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PART II

A SIMPLIFIED METHOD FOR DETERMINING RISER DIMENSIONS

*by H. F. Bishop, T. E. Myskowski and W. S. Pellini**

Part I of this paper appeared in the Number 1 issue of this Journal published in January. It contained information on the determination of minimum riser diameters and minimum riser volumes for simple shaped castings.

Risering of Sections with Appendages

Many castings consist of several attached sections of different thicknesses and geometries. In such cases it is the practice to place the riser on the largest section so that the thinner sections can be fed by the massive section which in turn is fed by the riser. The relative volume of riser metal needed for such castings is less than would be required for this same casting volume if it were distributed as a uniformly massive section. The reason for this is evident from the illustration in Fig. 12 showing the solidification and feeding conditions in castings consisting of 4-inch thick plate sections to which are attached parasite plate sections of different thicknesses.

The total volume of the parasite sections in Fig. 12 are equal and, hence, require the same actual amount of feed metal from their respective risers. However, the times at which the various sections demand feed metal are different. The half-inch sections become completely solid in about a minute and require all of their feed metal by this time. Calculations show that at one minute after pouring the 12-inch diameter riser (initial volume of 113 cubic inches for each inch of height) has solidified only to the extent that it has about 104 cubic inches of liquid metal in the riser for each inch of its height.

The solidification times required for 1, 2 and 4 inch appendages are proportionately greater and the volume of liquid metal in the risers at the respective solidification times are approximately 95, 78 and 50 cubic inches per inch.

A riser can supply a large volume of feed metal per inch of its height when there is a high early feed demand as for castings having thin appendages. When the appendages are thick, the riser must supply feed metal, not only to the appendages but to the parent section as well, at a late period in the solidification cycle when it is least able to do so; hence larger risers are necessary.

The timing of the feed demand is reflected in the shape of the riser pipe, Fig. 13, as sketched from actual risers on castings. The riser pipe tends to be wide and short for large early feed demands, whereas with large late feed demands it is narrow and deep.

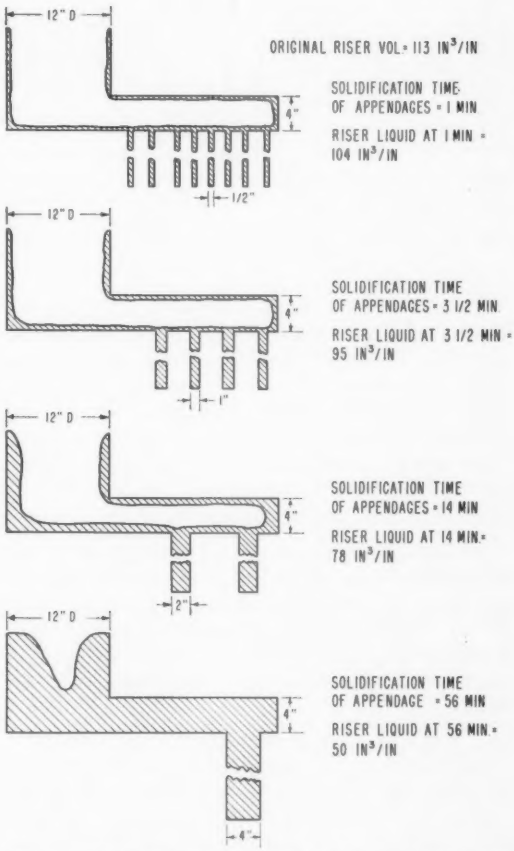

12" D
ORIGINAL RISER VOL = 113 IN³/IN
SOLIDIFICATION TIME OF APPENDAGES = 1 MIN
RISER LIQUID AT 1 MIN = 104 IN³/IN
4"
1/2"
12" D
SOLIDIFICATION TIME OF APPENDAGES = 3 1/2 MIN
RISER LIQUID AT 3 1/2 MIN = 95 IN³/IN
4"
1"
12" D
SOLIDIFICATION TIME OF APPENDAGES = 14 MIN
RISER LIQUID AT 14 MIN. = 78 IN³/IN
4"
2"
12" D
SOLIDIFICATION TIME OF APPENDAGE = 56 MIN
RISER LIQUID AT 56 MIN = 50 IN³/IN
4"
4"

Figure 12—Schematic illustration of how the time of high feed demand influences riser efficiency

Combinations of bar and plate shapes, where one shape is an appendage to the other, frequently occur in commercial castings; Fig. 14 shows examples. The minimum risers needed on such combinations of varying thickness ratios have been experimentally determined and pertinent data are shown in Table II. For each casting the volume of riser required to feed the parent section and the volume required to feed the appendages are determined separately.

* Metallurgical Division, Naval Research Laboratory, Washington, D. C.

The chart of Fig. 15 shows the portion of the parasite volume which is added to the parent volume when calculating risers. For example, for a parent bar with a parasite plate of half the bar thickness, approximately 0.85 of the parasite volume is to be considered as casting volume in the calculations.

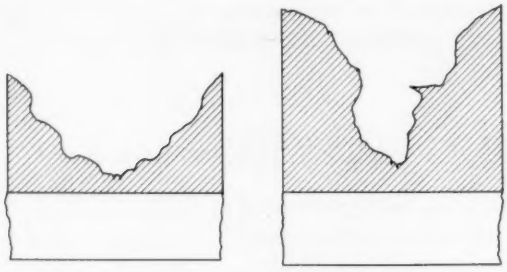


Figure 13—Reproductions of riser pipe. Left: 13-inch diameter 7-inch high risers on 4-inch plate with 2-inch bar appendages, i.e., high early feed demand. Right: 13-inch diameter 10-inch high riser on 4-inch plate with 6-inch bar appendages, i.e., high late feed demand

Regardless of the shape, thickness and volume of the appendages attached to the parent shape, the (L+W)/T factor, from which the relative riser volume is obtained, is determined only from the parent shape. In translating relative riser volume into actual riser volume the true volume of the parent shape plus the percentage of the parasite volume indicated in Fig. 15 is multiplied by the Rv/Cv factor.

An additional example to that provided in Fig. 15 is a casting consisting of a 3-inch thick bar appendage attached to a 6-inch thick plate; or a thickness ratio of 0.5. Referring to the plate-bar curve Fig. 15, it is indicated that 0.3 of the bar volume should be added to the plate volume in calculating the riser volume.

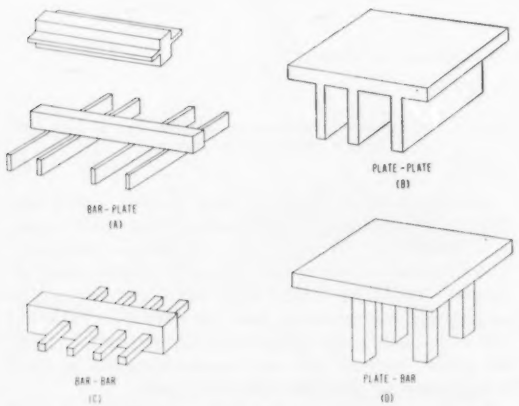


Figure 14—Idealized examples of casting combinations of different shapes and thicknesses

TABLE II—Minimum Risers Needed to Feed Parent Appendage Combinations

Casting Type	Min. D.-in.	Riser H.-in.	Riser Vol. Cu. in.	Riser Vol. for Append. Cu. in.
4″x4″x30″ Bar	7	5	190	—
+½″ Plate	7	6	230	40
+1″ Plate	9	4	254	64
+2″ Plate*	9	6	382	190
+2″ Plate**	9	5½	350	160
4″x4″x30″ Bar	7	5	190	—
+1″ Bar	9	3½	206	16
+2″ Bar	9	3¾	240	50
+3″ Bar	9	4	254	64
4″x20″x20″ Plate	13	5½	730	—
+½″ Plate	13	6½	860	130
+2″ Plate	13	8	1065	335
+3″ Plate	13	9	1200	470
4″x20″x20″ Plate	13	5½	730	—
+2″ Bar	13	6½	860	130
+4″ Bar	13	7¾	1030	300
+6″ Bar	13	8¼	1090	360

* Longitudinal Joint
** Transverse Joint

It can be noted from Fig. 15 that when the section differentials are great, the added riser volume to feed the appendages is very small and only a small fraction of the appendage volume need be considered. However, as the section differential becomes smaller the added riser volume increases proportionately. When the parent section and appendages have the same solidification time, then the entire appendage volume must be considered for purposes of calculating riser volumes. This condition would arise for the case of plate-to-plate or bar-to-bar combinations when the parent and appendage sections are of equal thickness; but for cases of bar-to-plate and plate-to-bar combinations the condition arises when the ratio of appendage-to-parent thicknesses is 0.6 and 1.6 respectively. If these ratios are exceeded it would indicate that the appendage section has a longer freezing time than its parent section - a condition which does not permit feeding of the appendage. In such a case, the riser should be placed on the appendage which would then be considered as the parent shape.

These facts concerning appendages impart further significance to Fig. 1 when it is pointed out that casting thickness does not indicate relative solidification times when different shapes are considered. Hence, if a casting combination exists where a 4-inch thick plate is attached to a bar having a cross section of 5 x 5 inches, the bar must be considered as the appendage because it has the shorter solidification time and the riser must be located on the plate.

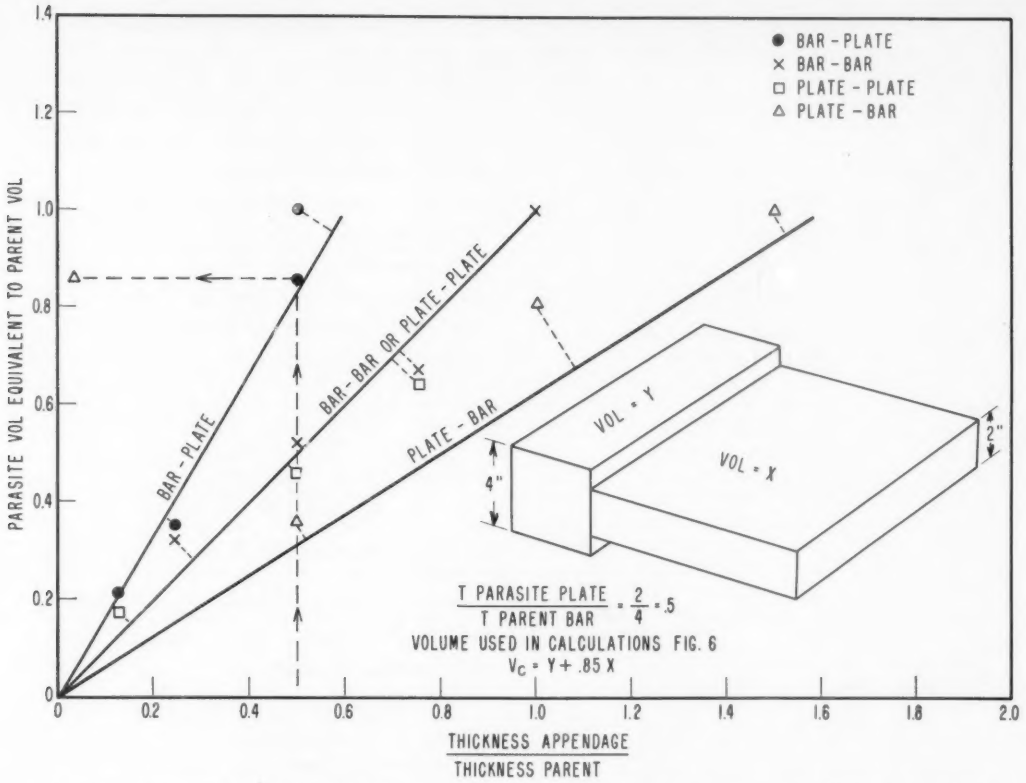

BAR - PLATE
BAR - BAR
PLATE - PLATE
PLATE - BAR
PARASITE VOL EQUIVALENT TO PARENT VOL
BAR - PLATE
BAR - BAR OR PLATE - PLATE
PLATE - BAR
VOL = Y
VOL = X
4"
2"
T PARASITE PLATE / T PARENT BAR = 2/4 = .5
VOLUME USED IN CALCULATIONS FIG. 6
Vc = Y + .85 X
THICKNESS APPENDAGE / THICKNESS PARENT

Figure 15—Percentage of appendage volume to be added to the parent volume to determine the minimum riser volume

Although the curves of Fig. 15 are indicated as single straight lines, they would properly be shown as bands, as in the (L+W)/T vs Rv/Cv charts, if more data were available since these minimum riser volumes are influenced by the same variables as were the simple casting shapes. Thus for any given foundry practice these curves should be considered as guides to be modified as examination of risers on such castings indicates.

Figures 16, 17 and 18 show sketches of castings having appendages for which risers were calculated.

Hot Spot Effects on Risering

Castings, or sections of a casting, are often of such design that their rate of solidification is retarded. Examples are X, L and T joints where the limited amount of sand at the internal corners creates a barrier to heat flow from the solidifying metal. Also, bushings have relatively small internal diameters and heat removal is retarded by the small volume of sand in the core. The time of solidification of such sections is comparable to flat sections of greater thickness than the geometric thickness of the complex section; hence it is expected that risers larger than those indicated by the (L+W)/T relationships would be required.

A number of X, L and T joints were cast, both with plate members and bar members, and the minimum riser volumes required to feed them are shown in Table III. Also shown in Table III are the minimum riser volumes needed to feed two sizes of bushings.

It was concluded that if the L, X and T sections are treated as parent-appendage combinations, no correction factor is required to increase the riser volume. This is evidenced by the fact that castings treated in this fashion, as they are in Table III, have minimum risers such that when the experimental Rv/Cv ratios are plotted against the (L+W)/T factors of the parent member of the castings, the points fall within the band of Fig. 4 - providing only those combinations with risers having heights not in excess of their diameter are considered.

In treating these shapes as parent-appendage combinations, the largest member of the section is considered as the parent shape from which the (L+W)/T and Rv/Cv factors are determined while the other member (in the case of the L and T shapes) or the other 2 members (in the case of the X shape) being considered as appendages.

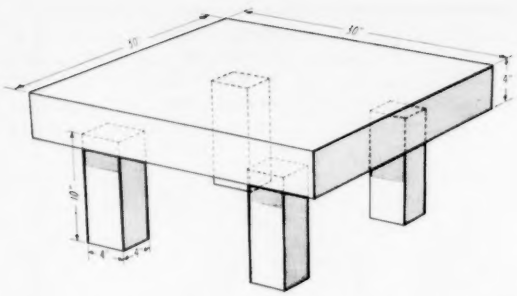


Figure 16—Minimum riser dimensions for a plate with bar appendages

Calculations For Figure 16

4" thick plate casting with 4" thick bar appendages

Vol. of parent plate = 30"x30"x4" = 3600 in³

(L + W)/T of parent plate = (30 + 30)/4 = 15

Rv/Cv (From Fig. 4) = 0.28

Vol. Appendages = 4(4"x4"x10") = 640 in³

$$\frac{\text{T (Appendage)}}{\text{T (Parent)}} = \frac{4"}{4"} = 1$$

Appendage Vol. used (Fig. 15) = 0.6

Riser Vol. = .28 (3600 in³ + .6 x 640 in³) = 1100 in³

Riser Dimensions (Fig. 5) = 14" Diam. 7" High or
13" Diam. 8½" High or
12" Diam. 10" High

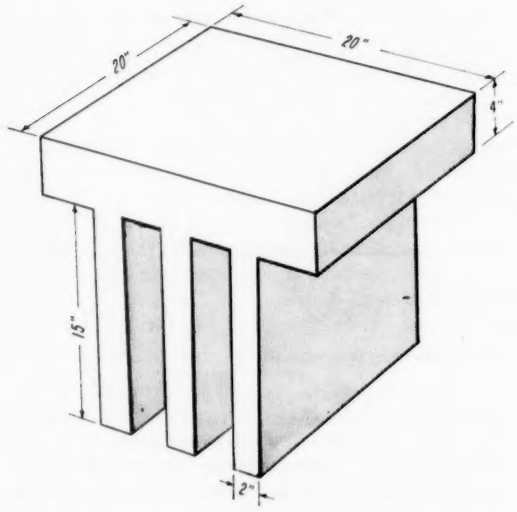


Figure 17—Minimum riser dimensions for a plate with plate appendages

Calculations for Figure 17

4" thick plate casting with three 2" thick plate appendages

Volume of parent plate = 20"x20"x4" = 1600 in³

(L + W)/T of parent plate = (20 + 20)/4 = 10

Rv/Cv (From Fig. 4) = 0.4

Volume appendages = 3(20"x15"x2") = 1800 in³

$$\frac{\text{Thickness (Appendages)}}{\text{Thickness (Parent)}} = \frac{2"}{4"} = 0.5$$

Part of Appendage Volume used (Fig. 15) = 0.5

Riser Vol. = .4 (1600 in³ + .5 x 1800 in³) = 1000 in³

Riser Dimen. (Fig. 5) = 11" Diam. 10½" High or
12" Diam. 9" High or
13" Diam. 7½" High

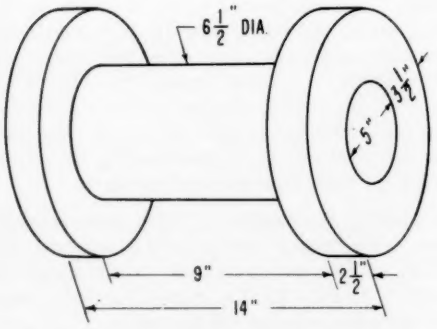


Figure 18—Minimum riser dimensions for a fitting fed with 2 risers (bar with a plate appendage)

Calculations For Figure 18

Fitting Fed with 2 Risers

Considered as a 2½" thick bar with ¾" thick plate appendage

Calculations for each Riser:

Vol. one Flange = $\pi(6^2 - 2\frac{1}{2}^2)$ x 2½ = 234 in³

Vol. ½ Barrel = $\pi(3.25^2 - 2.5^2)$ x 4½ = 61 in³

Medium Length of Flange = $\pi(5 + 3\frac{1}{2})$ = 27 in

$$(L + W)/T = \frac{27 + 3.5}{2.5} = 12$$

Rv/Cv (Fig. 4) = .35

$$\frac{\text{T (Barrel)}}{\text{T (Flange)}} = \frac{.75}{2.5} = 0.3$$

Barrel Vol. used (Fig. 15) = 0.5

Riser Vol. = .35 (234 in³ + .5 x 61 in³) = 92 in³

Riser Dimensions (Fig. 5) = 5" Diam. 5" High

TABLE III—Minimum Risers Needed for Castings with Restricted Heat Flow

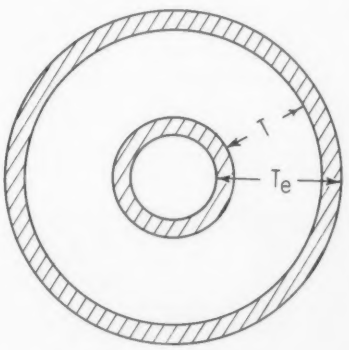
Shape	Parent in.	Appendage in.	RISER USED Dia. in.	RISER USED Ht. in.	MIN. RISER Dia. in.	MIN. RISER Ht. in.	Riser Vol. Cu. in.	Cast. Vol. Cu. in.	Rv/Cv	Cast. Soundness	Parent L+W/T	L+W/T_E
L Bar	4x4x20	4x4x16	9	6½	9	4½	285	575	.49	OK	6	—
T Bar	4x4x20	4x4x20	7	9	—	—	—	640	—	NG	6	—
T Bar	4x4x20	4x4x20	9	7	9	5½	350	640	.54	OK	6	—
T Bar	4x4x20	4x4x16	9	6	9	4½	285	575	.49	OK	6	—
X Bar	4x4x20	2(4x4x8)	9	5½	9	4½	285	575	.49	OK	6	—
L Plate	4x20x20	4x10x20	13	10	13	8	1070	2400	.44	OK	10	—
T Plate	4x20x20	4x10x20	10	12	—	—	—	2400	—	NG	10	—
T Plate	4x20x20	4x10x20	12	7½	—	—	—	2400	—	NG	10	—
T Plate	4x20x20	4x10x20	12	8½	12	8	900	2400	.38	OK	10	—
T Plate	4x20x20	4x10x20	13	8	13	7½	1000	2400	.42	OK	10	—
X Plate	4x20x20	2(4x20x10)	15	10	15	9	1600	3200	.50	OK	10	—
X Plate	4x20x20	2(4x20x10)	15	8	—	—	—	3200	—	NG	10	—
X Plate	4x20x20	2(4x20x10)	10	30	—	—	—	3200	—	NG	10	—
X Plate	4x20x20	2(4x20x10)	12	18	12	16	1800	3200	.56	OK	10	—
12″ Cylinder	12″ OD - 4″ ID		12	6	12	5¼	595	1200	.50	OK	9½	8
12″ Cylinder	10″ OD - 2″ ID		12	7	12	5	565	910	.63	OK	8	6½
12″ Cylinder	10″ OD - 2″ ID		12	5	12	5	565	910	.63	OK	8	6½

The need for a correction factor to provide additional metal in the riser to feed the hot spot is eliminated by this method of riser size determination which indicates smaller (L+W)/T factors (hence larger risers) than, in a sense, are actually present. For example, it would be logical to consider casting length as the sum of all members of the shape, in other words treat the shape as if its various members were reassembled into a flat plate. A larger shape factor and a smaller riser volume would thus be indicated and a correction factor would then be necessary to increase riser volume in order to accommodate the hot spot.

Risers for Sections Having Small Cores

There is no method of compensating for the restricted heat flow in cored areas as there is for the X, L and T joints. If a bushing with a small internal diameter were considered as a curved plate (which it actually is) and risered accordingly, the riser would be inadequate. This is evident for the case of the three bushings listed in Table III where the minimum risers which can be employed result in Rv/Cv ratios which are above the limits of the band of Fig. 4 when plotted opposite their (L+W)/T factors.

The problem can be resolved, however, by making use of correction factors determined in previous thermal studies[7] from which can be established the effective thickness, in regard to to solidification time, of bushing walls made with various core diameters. These correction factors and the method of using



CORE DIAM	REL. FT	T_e CORR. FACTOR k	EXAMPLE (4" WALL)
1/2T	1.38	1.17	4.7
T	1.25	1.14	4.5
2T	1.21	1.10	4.4
4T	1.08	1.02	4.1
FLAT PLATE	1.0	1.0	4.0

EFFECTIVE WALL THICKNESS = k (GEOMETRIC WALL THICKNESS) $T_e = kT$

Figure 19—Thickness correction factors used in calculations of minimum risers for cored castings

them are shown in Fig. 19; the bushing wall thickness when multiplied by the appropriate correction factor will indicate the thickness of a flat plate (T_E or effective thickness) which has the same solidifi-

cation time as the bushing walls. For example, a bushing with a 4-inch thick wall and a ½ T (2 inch) diameter core would have an effective wall thickness of 4 x 1.17 or 4.7 inches.

Core diameters of 4T or greater require no thickness correction and the bushing is treated as a flat plate. When the $(L+W)/T$ factor for bushings is calculated on the basis of effective rather than geometric thickness, a larger riser volume is indicated which corresponds to the minimum riser volume determined experimentally as being needed to feed the casting. In Table III it can be noted that when the Rv/Cv ratios are plotted against $(L+W)/T_E$ rather than $(L+W)/T$ they fall within the band of Fig. 4.

An example of a calculation for a minimum riser for a bushing is given in Fig. 20.

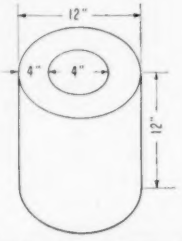


Figure 20—Calculation of a minimum riser for a bushing

Calculations for Figure 20

$Cv = \pi(6^2-2^2)x12 = 1200$ in^3
Median length $= \pi d = \pi 8 = 25$ in
For 1T core $K = 1.14$ (Fig. 19) and
Effective $T = 1.14 \times 4 = 4.55$ in.

$$\frac{L+W}{T_E} = \frac{25+12}{4.55} = 8.1$$

$\frac{Rv}{Cv}$ (Fig. 4) $= .47$

$Rv = .47 \times 1200 = 564$ in^3

Riser Dimensions 12" Diam. 5" High or
11" Diam. 6" High

Summary

The Steel Founders' Society's method of calculating riser dimensions for steel castings is based on empirical relationships between the "freezing ratio", i.e., surface area to volume ratio of the casting to that of the riser, and "volume ratio", i.e., volume of riser to volume of casting ratio. The minimum riser of a given diameter on a specific casting which will not result in under-riser shrinkage is one having dimensions such that the plot of the freezing ratio and volume ratio coordinates fall on an empirically determined curve.

The complicated calculations required for the determination of SA/V ratios according to SFSA Research Report No. 13 procedures may be eliminated by replacing SA/V as the shape index with a simple index based on the ratio of length plus width divided by thickness (shape index $= \frac{L+W)\cdot}{T}$ This index is related empirically to riser volume, expressed as a fraction of the known casting volume. The procedure entails the calculation of $\frac{L+W}{T}$ of the casting, reference to a graph relating this index to riser volume and finally the translation of riser volume to diameter and height dimensions.

Complex castings envolving joined sections or appendages require one additional step in the process of riser calculation. Auxiliary graphs are provided relating the volume of the appendage to additional riser volume required for the appendage per se. This volume is added to that required for the parent section on which the riser must be located.

Calculations for castings involving restricted heat flow (such as bushings) are modified by replacing the actual thickness in the $(W+L)/T$ determination with the effective thermal thickness. Tables of effective thickness for bushings of various core-to-casting wall diameter ratios are provided for this purpose.

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Figure 1 — Unit of eight high-vacuum pumps used for degassing

VACUUM DEGASSING AND VACUUM CASTING OF STEEL CASTINGS

Throughout the world it is universally known by technical men responsible for the making of steel, that the quality of steel depends on its gas content such as hydrogen, nitrogen, and oxygen.

The Bochumer-Verein Company, Bochum, Western Germany, has gained considerable experience in the production of high-quality steels through the use of: (1) the outgassing of steel in the ladle; (2) the pouring of steel through a vacuum, and (3) casting in vacuum. Initial studies were started in 1950 and now the production of vacuum steel is an accomplished fact. Ingots upward of 150 tons and steel castings for special requirements are being produced.

Molten steel can dissolve large quantities of gas. For example, 100 grams of liquid steel at 2900 degrees F can dissolve 30cc of hydrogen. But 100 grams of solid steel at 2200 degrees F can hold a maximum of 8 to 9 cc of hydrogen. Gases are responsible for pinhole porosity, blowholes, rock candy fracture, low ductility, shatter cracks, white spots, poor impact properties, embrittlement and probably other problems and defects.

Bochumer Verein has a unit of eight Leybold high-vacuum pumps and one backing pump with a capacity of 5000 cubic meters per hour as shown in Fig. 1. The pumps evacuate three low-pressure vessels. Two tanks 8 ft. diameter x 14 feet deep and one tank 14 ft. diameter x 30 feet are available in which large castings and ingots up to 150 tons can be poured. A vacuum of 2 mm mercury can be maintained above the molten steel.

Outgassing of The Ladle

The ladle filled with steel and slag is placed in the vacuum tank and the lid set in place. Figure 2 shows a ladle being lowered into a tank for degassing. Vacuum pumping at full speed follows and a pressure of 30 mm Hg can be obtained in 5 minutes by using all eight pumps. At this pressure the gas actively comes out of the molten steel, goes through the slag and escapes as luminous flames, together with particles of glowing slag. The process stops abruptly when air is again admitted to the vacuum tank.

The method is comparatively easy to carry out; however, the outgassing is only partial and temperature drop of the steel prevents the process from being used to its fullest extent.

(continued on page 13)

Figure 2—Ladle being lowered into low-pressure vessel

PROPERTIES OF 6130 (Cr-V) CAST STEELS

The Technical Research Committee of the Steel Founders' Society of America initiated an investigation of the physical properties of three of the more popular and promising lean alloy cast steels. In the first issue of the "Journal of Steel Castings Research" the data for TS-8130 cast steel was presented.

The properties of 6130 cast steel are presented in this issue. The 6130 cast steel is contemplated as a substitute for the popular manganese-molybdenum cast steels (8000 and 8400 series) because of the possibility of restrictions being imposed on the use of molybdenum during any future emergency period.

Three separate heats of 6130 cast steel were produced: one by the acid open-hearth process, one by the acid electric process, and one by the basic electric process. The chemical range desired was as follows:

C 0.27 - 0.33
Mn 0.70 - 0.90
Cr 0.75 - 1.10
V 0.10 - 0.17
Si 0.30 - 0.60

Each foundry cast keel blocks in one-inch sections and, in addition, the acid electric producer cast specimens of four-inch section. In all cases the foundries forwarded the keel block coupons in the as-cast condition to Case Institute of Technology for investigation.

Both the one-inch and four-inch cast coupons were heat treated by the laboratory prior to machining. The heat treatments employed for the one-inch sections were: (1) normalizing from 1675 degrees F and tempering at 1200 degrees F; (2) normalizing, water quenching from 1600 degrees F and tempering at 1000 degrees F; (3) normalizing, water quenching and tempering at 1200 degrees F. The four-inch sections were heat-treated similar to (1) and (3) above. All one-inch sections were held at temperature for 1½ hours and the four-inch sections were held at temperature for 4 hours. The tempering operation was followed by a water quench.

Chemical Composition

The ladle analyses of the three heats are presented in the table of Fig. 1. The composition of the three steels is within specifications except for the carbon content of the acid open-hearth heat.

CAST 6130

STEEL MAKING PROCESS	C	Mn	P	S	Si	Ni	Cr	Mo	V	Norm. Temp. °F.	Quench Temp. °F.
X Acid Open Hearth	.36	.74	.030	.048	.58	.05	.96	.02	.13	1675	1600
□ Basic Electric	.26	.78	.021	.025	.56	.047	.84	.038	.19	1675	1600
△ Acid Electric	.27	.72	.040	.043	.53	.09	.73	.03	.11	1675	1600

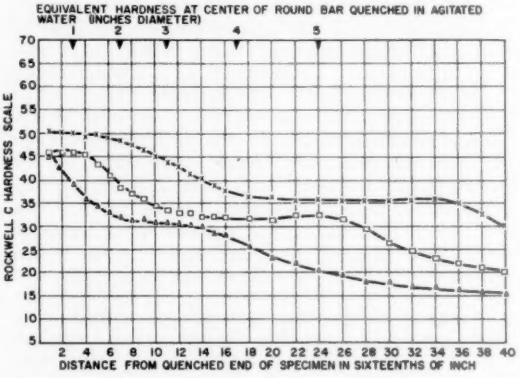


Figure 1—Jominy hardenability curves for various heats of cast 6130 steel.

TABLE I—Mechanical Properties of 6130 Cast Steel

Steelmaking Process	Strength Tensile psi	Strength Yield psi	Elong. in 2" %	Red. of Area %	BHN	Charpy V-Notch Ft/Lbs. at 74°F	Charpy V-Notch Ft/Lbs. at -40°F
Normalize (1675°F) and Temper (1200°F)							
Acid Open-Hearth	122,000	90,000	16.5	39.5	255	10.0	4
Acid Electric	102,000	67,000	21.7	44.0	192	17.5	8
Basic Electric	100,000	64,000	22.0	53.0	202	13.0	7
Water Quench (1600°F) and Temper (1000°F)							
Acid Open-Hearth	180,500	168,500	7.0	16.7	364	—	—
Acid Electric	148,000	137,500	10.5	23.6	321	—	—
Basic Electric	166,000	154,000	10.5	22.7	342	—	—
Water Quench (1600°F) and Temper (1200°F)							
Acid Open-Hearth	145,000	133,000	13.5	32.4	302	23.5	17.0
Acid Electric	135,000	120,500	14.5	31.8	286	14.5	9.0
Basic Electric	133,500	122,000	16.5	41.5	269	22.5	12.5

Mechanical Properties

The mechanical properties of the 6130 cast steels are listed by melting practice and heat treatment in Table I. The regular .505 inch diameter tensile and Charpy V-notch impact specimens were prepared from the approximate center of the one-inch sections.

The data in Table I indicates that the variations of the properties between heats of the 6130 steel are rather pronounced for each heat treatment. In most every case the acid open-hearth process shows higher values which is probably because of the higher carbon and chromium contents and not the process of melting.

The yield-tensile strength ratios for all heat treatments and section sizes are listed in Table II. For the one-inch section, the high values obtained with the quenched and 1000 degrees F tempered condition for this cast steel indicates this steel is essentially a through-hardening steel, at least for this section size. Increasing the tempering temperature to 1200 degrees F after water quenching resulted in a slight lowering of the ratio. This indicates the 6130 cast steel resists markedly the effect of tempering.

End-Quench Hardenability

The end-quench hardenability curves for the three heats are plotted in Fig. 1. The end-quench curves reveal considerable variation in the maximum hardness and hardenability between heats. The acid open-hearth heat shows the highest hardenability and surface hardness. This, no doubt, is because of the higher carbon and chromium content. The surface hardness of the basic electric and acid electric heats is quite similar but the basic heat is a deeper hardening steel. This may be the result of the larger total percentage of manganese, chromium and vanadium.

TABLE II—Yield-Tensile Strength Ratios for 6130 Cast Steel

Steelmaking Process	Section Size	Water Quench 1000°F Temper	Water Quench 1200°F Temper	Normalize 1200°F Temper
Acid Open-Hearth	1 inch	.93	.91	.74
Basic Electric	1 inch	.92	.91	.74
Acid Electric	1 inch	,92	.89	.66
Acid Electric	4 inch	----	.67	.60

Charpy V-Notch Properties of 6130 Cast Steel

The impact strength at room temperature and –40 degrees F is plotted in the form of bar graphs for all heats and section sizes in Fig. 2. The 6130 steel shows but little improvement in the impact values in the quenched and tempered condition over the normalized and tempered condition. In fact, the impact properties at –40 degrees F are unsatisfactory for the one-inch section as well as the four-inch section. This steel does not look very promising if low temperature impact properties are desired.

Effect of Mass

The effect of mass on the mechanical properties of 6130 cast steel was determined by taking test bars from approximately the center of the four-inch and one-inch test coupons. The data are listed in Table III.

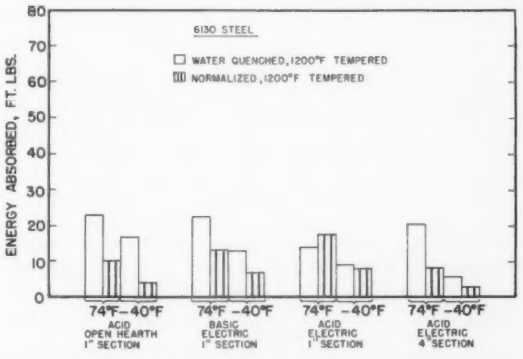
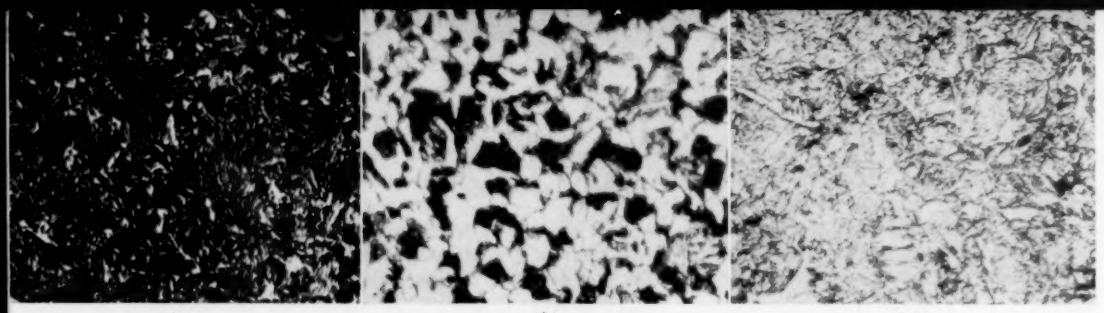


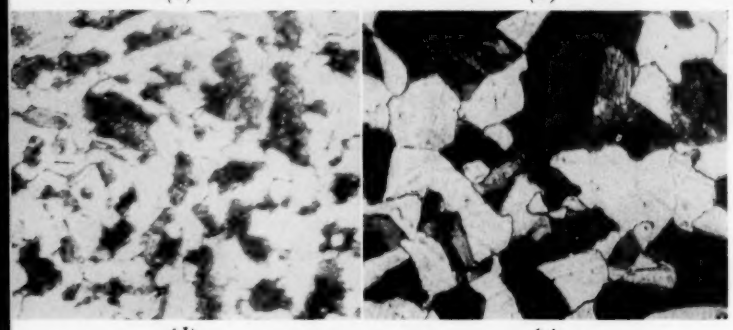
Figure 2—Charpy V-Notch impact strength of different grades of cast 6130 steel with various heat treatments. Tested at 74. and -40°F.

TABLE III—Effect of Mass on Mechanical Properties of 6130 Cast Steel Produced by Acid Electric Process

Section Size	Tensile Strength psi	Yield Strength psi	Elong. in 2" %	Red. of Area %	BHN	Charpy Impact Ft/Lbs. at 74°F	Charpy Impact Ft/Lbs. at –40°F
Normalize and Temper at 1200°F							
1 inch	102,000	67,000	21.7	44.0	192	17.5	8
4 inch	100,000	60,000	16.5	28.9	202	9.0	3
Water Quenched and Temper at 1200°F							
1 inch	135,000	120,500	14.5	31.8	286	14.5	9
4 inch	114,000	76,500	13.5	23.6	235	20.5	7



(a) (b) (c)



(d) (e)

Figure 3—Microstructures of 6130 Cast Steel (500X)

(a) 1" section random blocky ferrite plus pearlite

(b) 4" section random blocky ferrite plus pearlite

(c) 1" section tempered martensite, free ferrite

(d) 1" section tempered martensite, free ferrite

(e) 4" fine pearlite plus ferrite

The four-inch section has somewhat lower strength and ductility than the comparable one-inch section. As would be expected, the loss in strength and ductility was more pronounced in the quenched and tempered condition. In comparing the impact properties of the one-inch and four-inch sections for a given heat treatment, there is little variation shown between the two section sizes. In fact, at room temperature the four-inch section in the quenched and tempered state exhibited an improvement in properties.

The mass effect is to be expected and results from such factors as variations in cooling rates, soundness, and hardenability, etc.

Microstructure

Metallographic specimens were cut from the broken tensile bars of the acid electric heats for all heat treated conditions and section sizes. The structures are shown in Figure 3 (a and b) for the normalized and tempered condition; (c) for the quenched and tempered (1000°F) condition; (d and e) for the quenched and tempered (1200°F) condition.

The microstructures confirm the mechanical properties obtained and correlate with the findings of SFSA Research Report No. 15, "The Influence of Microstructure on the Mechanical Properties of Cast Steel." Section III of Report No. 16 deals specifically with the relationship between microstructure and mechanical properties of cast steels.

In the four-inch section the microstructure of random blocky ferrite plus pearlite should, according to Figure 25 of SFSA Research Report No. 25, give the best ductility. The somewhat lower values obtained are no doubt due to the specimens being taken in the plane of centerline shrinkage.

Conclusions

1—The 6130 cast steel offers promise as a substitute for the low-alloy molybdenum steels (8000 and 8400 series) if low temperature impact values are not essential.

2—The differences in properties between the one-inch and four-inch sections indicate that the effect of mass was relatively small.

3—Uniform hardenability and mechanical properties are obtained regardless of the steelmaking process. The differences that occured were due to the variations in the carbon and alloy contents of the acid open-hearth vs. the two electric processes.

METAL PENETRATION

This article is a summary of three reports issued by the British Steel Casting Research Association in the study of metal penetration into molded materials.

Metal penetration into molded materials has been studied to determine the effects of pouring temperature, degree of ramming, grain size, mold wash and clay contents of casting procedure. The research was conducted by laboratory experiments and comparing the results with actual shop practice. The sand mixtures used throughout the tests were prepared from two different silica sands of at least 99 percent purity with the grain distributions shown in Table I.

TABLE I—Grain Distributions of The Sand Mixes Tested

Screen	FINE MIX Percent on Screen	COARSE MIX Percent on Screen
22	—	28.7
30	—	46.6
44	1.0	16.2
60	1.8	2.0
72	5.0	1.6
100	40.0	1.5
150	42.5	1.0
Pan	10.7	2.7

Both the fine and coarse mix were prepared with 4 percent water and 4 percent bentonite as a binder.

Metal penetration of molten steel into a sand mold occurs instanteously. That is, the molten steel will not wet the silica mold until enough back pressure (ferrostatic metal head) is developed to force the metal between the sand grains. Once the metal flow begins it continues to run through the heated mold and stops when it is chilled by the cooler sand. Without a sufficient metal head, penetration will not be observed in an ordinary mold, therefore ferrostatic pressure is the determining factor for metal penetration.

Previous studies in SFSA Research Report No. 30 show that a mold is heated to near molten metal temperatures for only a shallow depth which points out the limiting depths of penetration.

Effect of Ramming

All work completed in this investigation has been based on the standard laboratory rammed specimen with the sand mixtures as discussed. Ramming a green sand mixture properly to orient the sand grains in such a way as to reduce the void space between them is the best method to minimize metal penetration.

An examination of metal penetration between sand grains is shown schematically in Fig. 1. The three sand grains meet and form a void space which is a possible channel for the molten steel if ferrostatic pressure is great enough to force the metal through the void space. The pore area, as shown by the solid circle, is the path of the liquid steel.

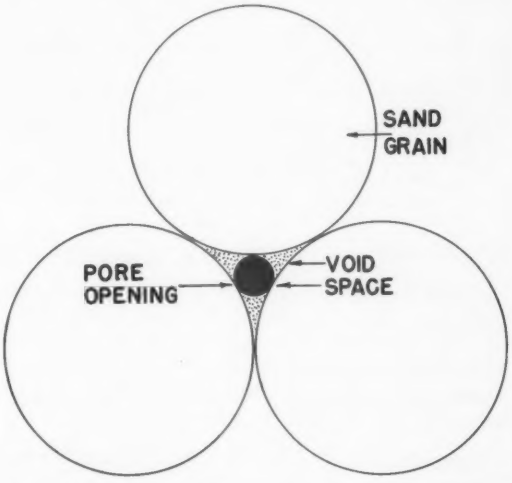


Figure 1—Schematic drawing of the path of molten metal between grains

The molten metal does not follow into the complete void opening as shown by the dotted and solid area because the metal will not wet the surfaces of the silica grains. Theoretical calculations based on the radius of the pore-opening (solid circle) agrees closely with the actual pressures needed to initiate metal flow.

If a sand mold is rammed loosely, the effect will be to increase the diameter of the pore thereby increasing the tendency for the metal to penetrate. Harder ramming, on the other hand, will tend to increase density of the mold surface and likewise reduce any metal penetration.

The variables in mold practices are many especially in hand rammed molds. To give a true picture of what the results are of a hand rammed mold, a vertical wall 6 by 14 inches was examined after ramming by an experienced molder. The surface was found to vary from 1 to 10 standard laboratory rams which is an extremely wide distribution when controls are to be maintained to prevent metal penetration.

Figure 2 presents the variation of mold surface densities with packing as measured with standard rams.

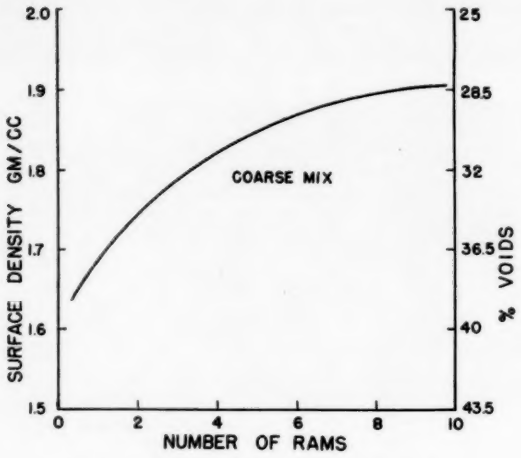


Figure 2—Effect of ramming on surface density

Grain Size

Penetration can be reduced by the use of a smaller grain size sand. The effect that the smaller grain size has on reducing penetration pressures is that it reduces the pore radius as shown in Fig. 1. With a smaller pore radius more pressure is needed to force the metal into the mold.

Experiments have shown that no difference exists between a wide or narrow grain size distribution range but the major factor is the mean grain size. Figure 3 shows the penetration pressure needed for iron at 1600 degrees.

As can be seen from Fig. 3 there is a proportionate increase in penetration pressure with the average grain size. However, as the grain size becomes smaller than AFS 100 the proportionality does not exist. The benefits obtained by using a finer grain size (reduced pore radius) is offset by an increased pore radius due to sintering action. These small grains of a large surface area and small volume are more prone to sintering than the large and more refractory grains.

Temperature

Increased pouring temperature has a twofold effect: (1) sand sintering action is greater the higher the temperature, and (2) the surface tension of the steel is lowered at the higher temperatures. Because the surface tension is reduced at the higher temperatures it is easier for the metal to flow into the pores of the sand mold. Figure 4 illustrates how a decrease in penetrating pressure is effected by an increase in metal temperatures. The work was conducted with molten iron.

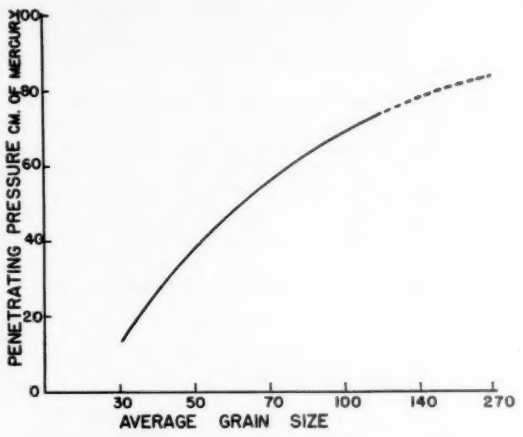


Figure 3—Effect of average grain size on the pressure of penetration

Clay Content

At low binder contents (below critical content) metal penetration is more pronounced at low percentages of clay binder because of insufficient bindering of the grains. Once the sand has been mixed with the minimum amount of clay to form a satisfactory mixture, further additions of clay have little effect in preventing metal penetrations. This has been credited to the molten metal ability to wash the clay from out of its path, therefore creating possible metal penetration.

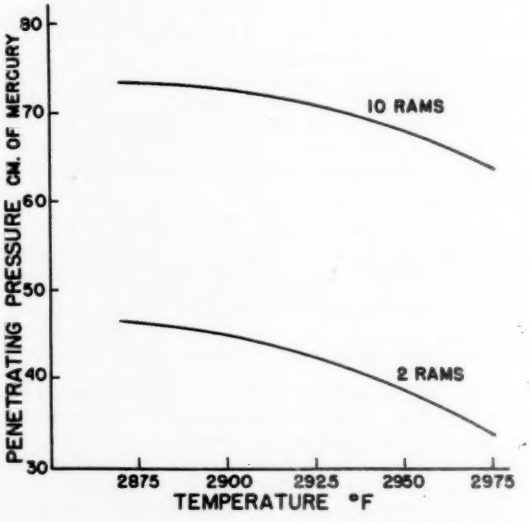


Figure 4—Pouring temperature's effect upon penetration

Effect of Washes

Silica flour washes have very little influence in retarding metal penetration. This is because of the fact that as the wash dries, cracks are formed which are larger than the sand pores, and therefore it is easy for metal to flow into the mold face. Cracking tendency has been found to be more prevalent in zircon washes than in silica washes.

Work presently in progress has indicated that washes based on low melting point eutectic compositions are most promising. Viscous high-silica low-alumina compositions near the tridymite-mullite eutectics were found to be effective in preventing penetration and producing a clean strip.

Laboratory Tests

Information on the development of a foundry laboratory test is given in the next article in this issue of the Journal.

Summary

Resistance to penetration may be increased by:

1. Harder ramming.
2. Use of a finer sand.
3. Control of pouring temperatures.
4. Possible use of low melting eutectics for mold washes.
5. Closer control of foundry molds by a simple laboratory test.

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"Metal Penetration and Sand Burn - on in Steel Castings: Their Causes and Prevention" by T. P. Hoar: West of Scotland Iron and Steel Institute, 1952.

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Vacuum Degassing (continued from page 7)

The Pouring of Steel Through a Vacuum

This method requires an empty ladle to be placed in the vacuum tank, the lid is put in place and the tank evacuated.

The molten steel is tapped from the furnace into a ladle and this ladle is then moved over the lid of the vacuum tank. The steel is then bottom poured into a pouring basin attached to the lid. The molten steel melts an aluminum sheet disc in the lid of the vacuum tank and the steel falls through a vacuum into the second ladle. The molten metal stream, upon entering the low-pressure area of the vacuum tank, is more or less torn to bits, depending upon the degree of vacuum, the diameter of the stream and the rate of pouring. Every effort is made to break up the stream into droplets during the pouring operation in vacuum.

High-speed photography at 1800 to 5500 frames per seond shows the steel droplet to swell up and explode, then collapse and continue its fall. Observation through a quartz window built into the lid of the vacuum vessel shows that a mass of individual drops are falling in a shower of tubular form.

The transfer time of pouring for 40 tons of steel is about eight minutes and the steel loses about 50 degrees F during this time.

The second ladle is removed from the vacuum tank and the steel is cast in air by conventional methods. The metal thus obtained shows a considerable reduction in the content of hydrogen and other gases. This is the practice which is followed with the pouring of steel castings and it is used particularly in conjunction with highly alloyed steel castings and high silicon transformer steel.

Casting in Vacuum

This method is used for the casting of ingots and has been found most valuable for the casting of 150 ton ingots used in the manufacture of large turbine shafts and other heavy-duty single-shafts.

Non-destructive testing at intermediate stages of the production of the steel from the vacuum process by radiographic and ultrasonic methods, together with conventional test specimens, has shown that the steel is definitely superior to conventional castings.

The gas content of a large number of melts degassed in the vacuum tanks has been analyzed. The following average values were obtained:

H_2 about 20 percent by volume
CO about 45 percent by volume
CO_2 about 5 percent by volume
N_2 balance

During the last two years Bochumer-Verein has produced over 10,000 tons of high-grade steel of various compositions, primarily in ingot form but to a lesser degree as steel castings. A marked improvement in the quality of ingots and special steel castings has resulted according to the Bochumer-Verein Company.

A SIMPLE LABORATORY METHOD for MEASURING PENETRATION CHARACTERISTICS OF SAND MIXES

This article is a summary of the developments that have been made by the British Steel Casting Research Association in evolving a simple test procedure for measuring a molded sand's resistance to metal penetration.

Introduction

Steel foundries have at their disposal very few testing procedures available for measuring the degree of ramming a sand mold. Indentation hardness and scratch testing methods often give misleading results as the tests depend upon time after molding. In Fig. 1 it is seen how the mold hardness of a silica sand, bentonite, cereal and water mixture varies with the time of testing after molding. The critical factor in obtaining a representative mold hardness number is the time involved between the test and molding of the mixture.

The test developed in England for mold penetration value basically consists of measuring the amount of negative pressure needed to suck a quantity of mercury through a sand sample cut from a mold rammed in the foundry. Experience with the test has enabled the British research workers to predict the height of a molten steel column that will penetrate a rammed sample of a specific penetration pressure value as obtained by the test.

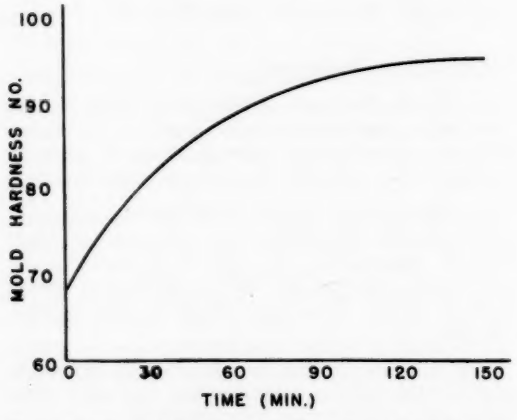

Figure 1—Effect of time on the mold hardness indentation test

Test Procedure

The apparatus used for measuring penetration pressures is shown in Figure 2.

Sand samples for testing are removed from the mold-face by means of a one-inch diameter cork-borer. The borer is inserted into the mold for a depth of one inch, a slight twist is given, and then the sample is removed. Since the test measures the penetration at the surface, the bottom should be placed on a stripping post and removed with care so as not to disturb the top layers. The samples are placed to dry in an oven for 30 to 40 minutes at 250 degrees F and then are allowed to cool.

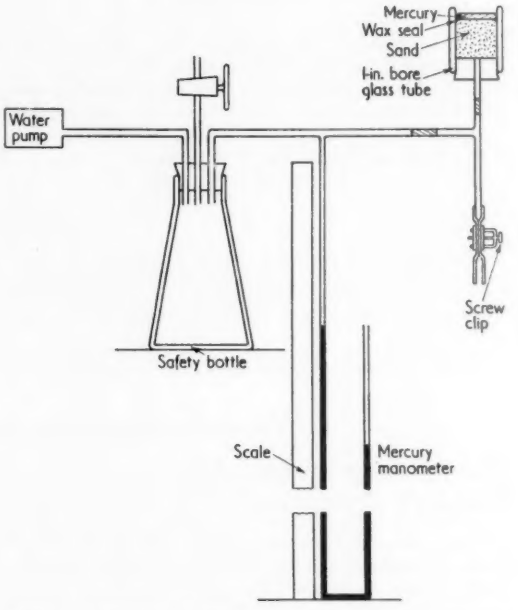

Figure 2—Apparatus for measurement of the penetration pressure

The dried samples are then placed in a 1-inch diameter glass cylinder with the mold-face upwards. The top edge around the sample next to the glass cylinder is sealed with liquid paraffin wax that is heated to 200 degrees F above its melting point and applied with a small brush. The seal should be made at least one-eighth of an inch below the surface to prevent the mercury from going down along the surface of the glass tube. False readings will be obtained if the seal is not complete.

A standard depth of mercury is poured on the test specimen at the beginning of each test. The mercury may be easily measured by having two metal probes, one of which rests against the mold surface and the other should be two tenths of an inch above the first probe. The probes are wired into a circuit which includes a dry cell to ring a bell so that a standard depth of mercury (0.2 in.) may be measured. Suction is then built up beneath the

sample by means of a water exhaust pump as shown on the diagram of the aparatus. The manometer will begin to register the difference in pressure and will keep on increasing until the penetration pressure is reached at which time the bell stops ringing. At this time the pressure of the manometer should be recorded. The mercury will completely penetrate the sample and can be recovered.

Pure mercury cannot be used in the test because it will be contaminated by the bentonite and the sand samples and will result in a variation of test results. Therefore, it is necessary that contaminated mercury be used. This may be done by sucking the mercury repeatedly through a bentonite-sand mixture until a constant value is recorded on the manometer for the penetration pressure.

The penetration pressure recorded for six samples of the same mixture that were given ten standard rams is presented in Table I.

TABLE I—Results of Repeated Tests

Sample No.	Penetrating Pressure (cm) of mercury	
1	6.3	
2	5.9	
3	6.3	MIXTURE
4†	4.5	Silica Sand 92%
5	6.6	Bentonite 4%
6	6.4	Water 4%
Avg.	6.3	

† not used in average incomplete wax seal.

Sample 4, Table I, was considerably below the value obtained from the other tests; the reason being that the wax seal was not completed properly and so it broke during the test. Reproduction of the test should be within plus or minus 0.5 cm.

The penetration pressure of a mold can be related to the AFS standard ramming technique. The standard AFS rammer can be adopted to the one-inch diameter specimen by reducing the original 14-pound weight and 1.9 inch diameter plunger to a 3.7 pound weight and a plunger diameter of 1 inch. By making these changes the one-inch sample will receive the standard ram thereby making it possible to compare results.

The penetration pressure of a laboratory rammed one-inch specimen can be compared to the penetration pressure of a one-inch mold sample to decide on the amount of ramming necessary to prevent penetration in the production mold. This method utilizes the penetration pressure test to convert to standard rams so that foundry control can be initiated. A curve similar to Figure 3 can be constructed for each sand mix so that the information obtained through testing control may be summarized into a usable form.

For example, it was found that for a particular sand mix mercury penetration occurred when the penetration pressure was 6.3 cm. This condition was equivalent to steel penetration when the column of molten steel above the sand was 25 inches. However, if the sand was rammed so that mercury penetration did not occur until the penetration pressure was raised to 7.0 cm of mercury, then the 25 inch column of molten steel did not penetrate the sand. Control was then established for this sand mix for future designs that require a 25-inch head of metal. Mold ramming is performed so as to give a penetration test value of 7.0 cm of mercury, or better.

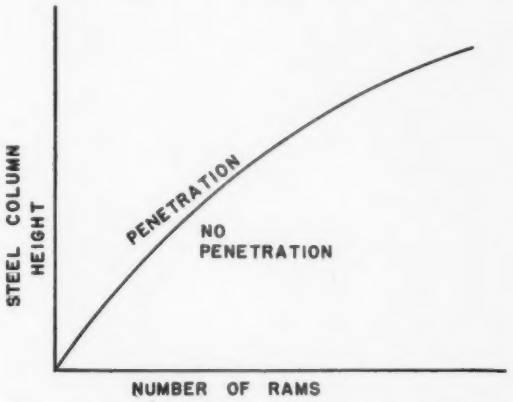

Figure 3—Example control chart for determining the amount of ramming necessary

Conclusions

The disadvantage of this test is that it is partly destructive because the molds will need to be repaired after the samples are drawn; but this, in many cases, should be a simple job.

The main advantage of using penetration pressure in preference to the mold indentation test is that consistent results will be obtained.

Although the penetration test is performed at room temperature it can be successfully used for determining the resistance to penetration needed at pouring temperatures. Caution should be exercised when interpreting results as a higher than normal pouring temperature will penetrate through the mold at lower head pressures.

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REDUCING STRESS CONCENTRATIONS IN WELDMENTS BY UTILIZING CAST-WELD CONSTRUCTION

(by E. J. Wellauer †)

Changes in section dimensions can often lead to stresses which are 1.2 to 3 times the nominally computed stresses. Design changes which increase the fillet radius in comparison to the section size can provide a greater increase in strength than is economically possible by changes in alloys or heat treatment.

This principle can be illustrated by a study of stress concentrations in a typical cantilever beam such as exemplified in a weldment of perpendicular arms as shown in Figure 1. As can be seen, the result of welding raises the actual stress 70 percent above the nominally calculated stress of the junction of the beam and vertical section. Figure 2 substitutes a casting for the vertical section and removes the weld away from the stress concentrated area. Since the vertical section is designed as a casting, the proper radius at the section junction can be employed which yields a lower stress concentration factor of 1.2.

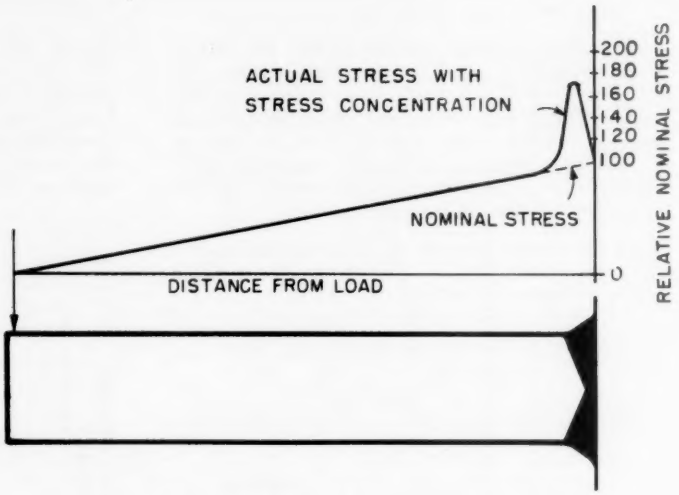


Figure 1—Welded construction showing stress concentration

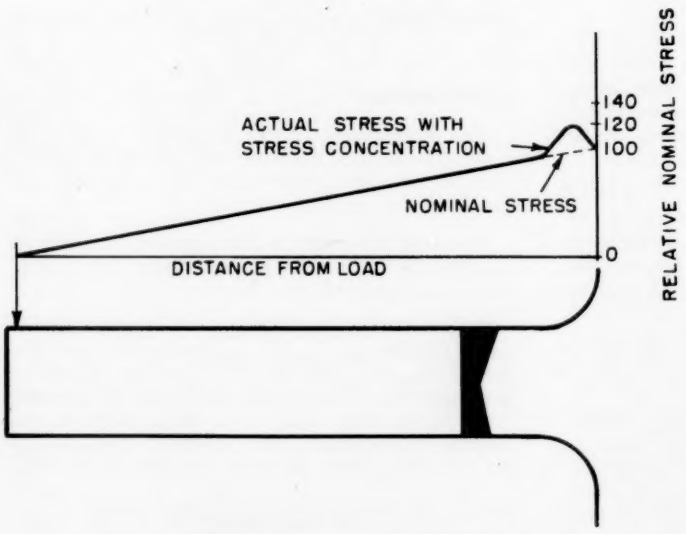
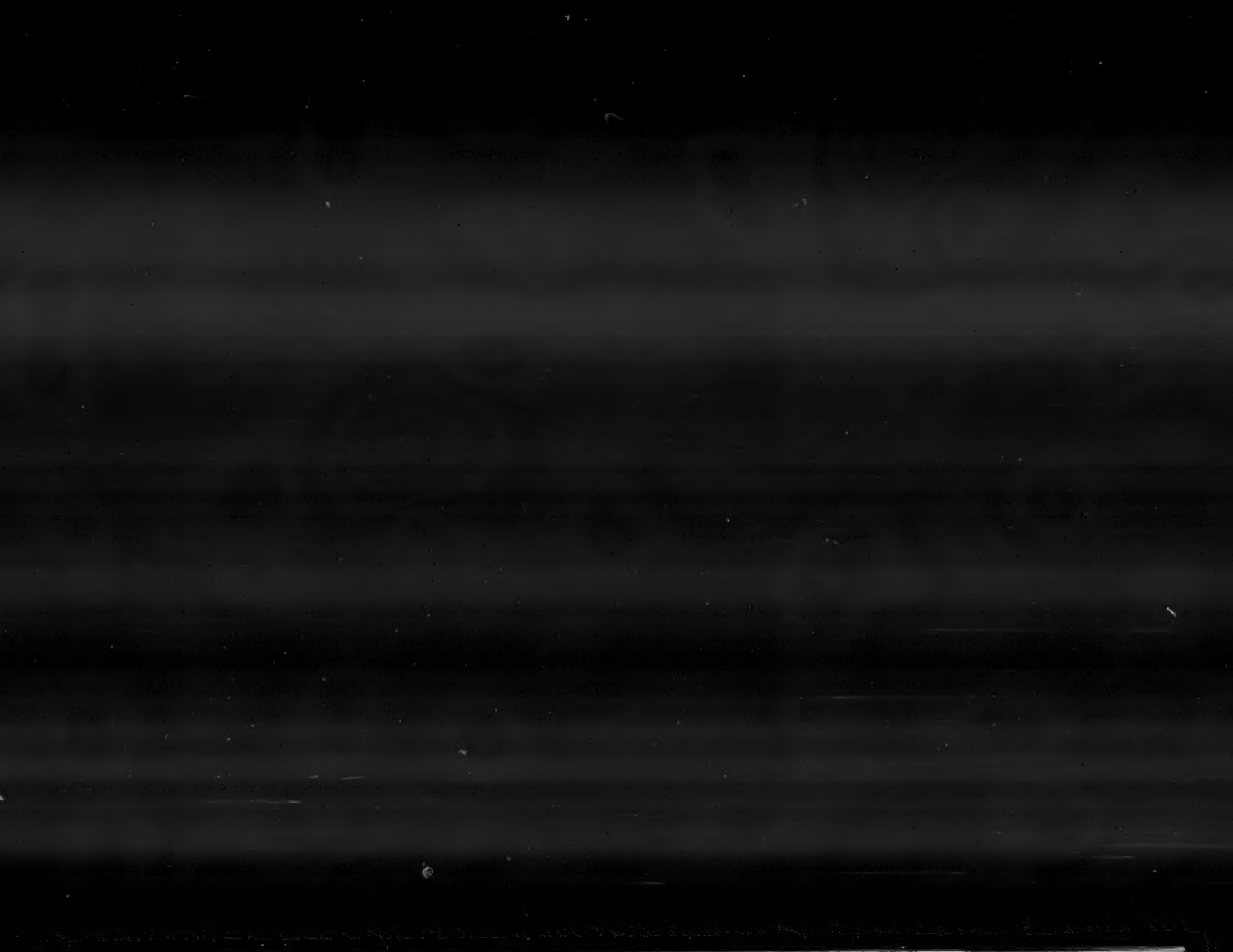


Figure 2—Cast-weld construction with the location of the weld away from stress concentration

The weld being removed from the vertical junction can itself contribute stress concentration at its new location but this can be minimized by grinding the weld flush as shown in the figure. The total effect that the weld may have on the stress pattern after grinding will not be injurious because the nominal stress at its new location is somewhat lower than at its previous location.

The reduction in stress concentration at joined sections is an outstanding advantage of cast-weld construction over joining of wrought members by welding.

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